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Determination of Allowable Hydrogen Permeation Rates for Launch Vehicle Propellant Tanks

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This paper describes the development of methods to determine allowable hydrogen permeation rates for composite cryogenic tanks for launch vehicles. Background information on the development of hydrogen permeation testing is provided and permeation testing apparatus developed recently is described. Calculated results for a variety of tank designs and permeation failure modes are included. Tank designs are considered for both reusable and expendable launch vehicles. Failure modes related to system performance and to specific design features are addressed. A comparison of the calculated allowable permeation rates is made to recent permeation test data obtained on the NASA Space Launch Initiative program.

I. Introduction

THE extremely high value of structural weight reduction in launch vehicles has generated ongoing interest in the development of composite cryogenic propellant tanks. Development began with the National Aerospace Plane (NASP) program in the late 1980s and continued through several years of work on single-stage-to-orbit (SSTO) vehicle studies and technology development, including the production of a complex, multilobed composite tank for the X-33 program. In November of 1999, the X-33 tank failed during ground testing at the NASA Marshall Space Flight Center. The outer wall of one section of the honeycomb sandwich structure disbonded explosively as the tank was warming up after draining. An investigation indicated the failure was a result of excessive hydrogen permeation into the open honeycomb cells in the interior of the sandwich tank wall. Hydrogen that had permeated through the inner face sheet from the inside of the tank and, perhaps, liquid air that had been cryopumped in from the outside, could not escape quickly enough from inside the wall as the temperature increased. The resulting pressure increase within the wall eventually caused the debonding of the honeycomb core and the inner face sheet [1].

Hydrogen permeation had been studied with coupon testing in the early days of NASP and the results had been encouraging enough to proceed with unlined composite tank development. Permeation work was dormant for several years while other development issues, such as detailed design and analysis of joints and other features, large-scale tank manufacturing technology, subscale demonstrations, etc., received more attention. The failure of the X- 33 tank, however, brought the permeation issue back to the forefront and spawned a great deal of work on the subject over the last several years.

There is a large difference in the coefficient of thermal expansion (CTE) of the resin matrix and the carbon fiber in a composite laminate. Permeation is facilitated by microcracks that develop in the resin as a result of the stresses that arise there from the CTE difference and the extremely cold temperatures combined with mechanical loading from tank pressurization and vehicle body loads. Efforts at NASA, the U.S. Air Force, universities, and industry have been undertaken to predict and measure the nature and extent of the resin matrix microcracking. A corollary goal of some investigators is to find the optimum combination of resin matrix material, fiber, liner (if necessary), ply thickness, layup architecture, and processing technology to produce a composite laminate with the greatest resistance to microcracking and therefore the lowest permeability.

Although much has been done in the areas of predicting, measuring, and understanding resin matrix microcracking and the permeation that results from it, very little has been done to determine allowable permeation rates. But they must be established if the predicted or measured permeation rates are to be adequately judged.

Methods for determining permeation rate allowables for several types of failure modes are discussed next, followed by example calculations for launch vehicle tank applications and a comparison of the allowable rates to recent test data.

II. Determining Allowable Permeation Rates

Allowable permeation rates are difficult to determine in advance of vehicle development because they vary with different vehicle architectures and mission profiles. Even with a fixed architecture and profile, they vary with time and with location on the tank due to various failure modes. Furthermore, we cannot simply pick the minimum calculated allowable as the driving rate because the tank wall laminate is subject to different temperatures and stress states in the various failure modes and these differences affect the permeation rate. Thus the tank wall laminate might meet a demanding permeability requirement for one mode at a relatively benign temperature and stress, but fail to meet an apparently easier requirement for another mode which is not actually easier because the wall laminate is under more extreme temperature and stress.

In the early days of composite hydrogen tank development, work proceeded without specified permeation rate allowables and materials were screened using the detection limit of the permeation test apparatus itself. Serious efforts to determine realistic allowables were not undertaken because of the difficulties and uncertainties described above and because of the difficulty of obtaining representative test data.

Under the efforts in NASA's Space Launch Initiative (SLI) program a few years ago, a significant body of permeation rate data was obtained on a variety of materials from testing conducted at the NASA Marshall Space Flight Center and the Southern Research Institute in Birmingham, Alabama. SLI was a program to develop reusable launch vehicle (RLV) technology in the wake of the lessons learned from the X-33 program. The methods and initial results of the permeation rate allowables development described next were also a result of SLI.

The permeation failure modes that have been considered in this analysis have been divided into two types: those that are independent of tank wall configuration and those that are design dependent. Independent modes are related only to system performance. Design-

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dependent modes can be related to system performance or to a design-specific failure mode but are related to some tank design feature in one way or another. The analysis for reusable vehicles was based on a Boeing vehicle configuration for the SLI program that consisted of a bimese assembly of a reusable winged booster and orbiter. Allowables were calculated for the orbiter only, because it has more failure modes and they are more demanding. Representative allowables for expendable vehicle tanks were calculated for the upper stage hydrogen tank of the Delta IV vehicle for a variety of missions and for the Delta IV first-stage common booster core (CBC) vehicle.

A. Failure Modes Independent of Tank Wall Design

1. *Operations Needs During Ascent*

Hydrogen permeation through the tank walls during ascent and orbital insertion must be low enough so that sufficient fuel is available to complete the mission. The maximum allowable permeation rate necessary to meet this requirement is easily calculated by establishing a budget for total permeation in terms of fraction of propellant load and dividing that amount by the total tank surface area and the elapsed time from final tank top off to engine cutoff.

The allowable loss from permeation has been set at 0.25% of tank volume. This figure was based on discussions with Boeing launch vehicle propulsion personnel about the amount of propellant typically budgeted for residuals (unusable and undrainable). Residuals optimized for performance on fully mature vehicles typically range from 0.25 to 1.0%. Thus, 0.25% seems reasonable for a permeation loss allowable.

During the propellant loading and on-pad hold phases of the prelaunch period, the hydrogen tanks are frequently venting to prevent excessive buildup of ullage pressure. Losses due to this venting are continuously replaced by topping off the tank up to within moments of launch. For the calculation of permeation allowables for this failure mode, the time intervals were set based on the assumption of two minutes from final top off to launch plus the time from launch to main engine cutoff (MECO) for boost stages or second stage engine cutoff (SECO) for upper stages.

As might be expected from the short duration of boost stage operations, the permeation rate allowable turns out to be fairly high (easily met) for boost stages. As the calculated results of Table 1 show, the allowable is an order of magnitude lower for upper stages on nominal missions and 2 orders of magnitude lower for upper stages on extended duration missions. It should be noted, however, that if the amount of fuel set aside for permeation loss can be increased from 0.25%, the allowable permeation rate will increase in proportion.

2. *Explosive Mixture in Compartments*

All launch operations require that mixtures of hydrogen and oxygen gas, particularly where potential ignition sources are present, cannot be allowed to exceed explosive limits. The areas of concern include intertank structures, engine sections, interstages in which upper stages are suspended, payload adapters above the upper stages and, for reusable winged vehicles, the fairings between the tanks and the wings.

Table 1 Maximum allowable permeation rates to retain sufficient fuel for launch operations with a budget of 0.25% of fuel load for permeation losses. GTO stands for geosynchronous transfer orbit and LEO for low Earth orbit

Tank	Allowable permeation rate, $scc/s/cm^2$
Delta IV upper stages	
Extended mission	0.00574
Nominal specification	0.0181
Heavy dual manifest to GTO (Eastern range)	0.0319
Heavy to GTO (Eastern range)	0.0332
$M + (5, 2)$ to GTO (Eastern range)	0.0336
$M + (5, 4)$ to LEO (Western range)	0.0508
Delta IV CBC	0.487
SLI reusable booster	0.868

The task of keeping the gas mixture below the explosive limit in these compartments is greatly aided by a high volume gaseous nitrogen purge flow that is supplied by ground support facilities up to within moments of launch. The Delta IV CBC nitrogen purge flow rates are on the order of 500 standard cubic feet (scf) per minute for each compartment. The purge gas has two purposes: to prevent the development of an explosive mixture and to maintain the area within a prescribed temperature range to assure proper operation of mechanical and electronic components. The purge gas can be prewarmed to help prevent the extremely cold temperature that would develop in the compartments due to the proximity of the cryotanks.

As a result of the purge flow, the atmosphere in these compartments, as indicated by Delta IV CBC and other data, is greater than 99.9% nitrogen when the purge is cut off moments before launch. Furthermore, from the moment of launch, the compartments vent their contents as the vehicle gains altitude, reaching essentially zero pressure in just a few minutes. Therefore both hydrogen and oxygen must leak into the compartment and at sufficiently high rates to outpace the venting in order to reach the necessary proportions to create an explosive hazard.

The well-known explosive limit for hydrogen in air is 4%. This is for air at atmospheric pressure; in other words, gas at 14.7 psia that is 4% (by volume) hydrogen, 20% oxygen, and 76% nitrogen. Although many references cite this limit, no information has been found on explosive limits at reduced oxygen concentrations (reduced either by reduced pressure or higher nitrogen content at atmospheric pressure). But it stands to reason that reduced oxygen concentration should require a corresponding increase in hydrogen concentration to create the same hazard. Two cases are thus considered in this analysis. In the first case, for reduced oxygen concentration, we assume hydrogen concentration must be higher in inverse proportion based on the "4% in air at atmospheric pressure" starting point. In the second case, we make the conservative assumption that sufficient oxygen will be present regardless of the purge gas and the atmospheric pressure limit for hydrogen applies at all times. For these calculations, the volumes of the compartments are fixed, and the gas concentrations and mixture limits are worked in terms of standard cubic centimeters (cubic centimeters of gas at standard temperature and pressure, or scc, converted from projected compartment conditions using the ideal gas law) present in the fixed volume.

To calculate the allowable permeation rates, spreadsheets were created that perform a stepwise analysis of the contents of the compartment from launch to the time at which the compartment pressure reaches approximately zero. Data from the Delta IV CBC analysis indicate that a linear reduction in pressure from atmospheric to zero in that time is a reasonable assumption. For the first case, an oxygen leak rate, in scc/s, must be assumed and inserted in the spreadsheet, along with a hydrogen leak rate. The spreadsheet accounts for hydrogen and oxygen permeating into the compartment and for all three species (including the nitrogen left over from the on-ground purge), leaving it through venting and compares the amounts of hydrogen and oxygen present every second to the amounts required to satisfy the assumed mixture relationship for the explosive hazard. Complete mixing is assumed. The input hydrogen leak rate is adjusted until the lowest rate is found that manages to create the explosive mixture near the midpoint of the ascent. This iterative procedure must be done separately for each oxygen leak rate assumed. The final hydrogen leak rate, in scc/s, is converted to a permeation rate, in $scc/s/cm^2$, using the surface area of the tank or the appropriate fraction of it. Based on Delta IV specifications, leakage through tank closure joint hardware has been estimated at 20 standard cubic inches per minute (scim) for both tanks and accounted for in the analysis. This value was selected based on discussions with cryogenic valve specialists and is reasonable for the hydrogen tank, conservative for the oxygen tank. This amount of leakage turns out to be insignificant compared to the basic wall permeation required from each tank to approach the mixture limit. For the Delta IV engine section, hydrogen and oxygen leak rates for the RS-68 engines were also accounted for.

For the first case analysis, in which we assume the hydrogen concentration must be higher than the standard 4% limit if oxygen concentration is reduced, a high oxygen permeation rate is typically required. At lower rates, sufficient oxygen simply will not accumulate to satisfy the mixture relationship, regardless of the hydrogen permeation rate. For example, Fig. 1 shows portions of the spreadsheet used to calculate the allowable hydrogen permeation rate for the Delta IV 5-m upper stage hydrogen tank to maintain a safe mixture in the interstage. As indicated, an oxygen permeation rate on the order of 1.5 $scc/s/cm^2$ is required. Even at this rate, a significant hydrogen leak rate of 0.75 $scc/s/cm^2$ is also required. A plot of the gaseous contents of the interstage during ascent with these permeation rates is given in Fig. 2.

It should be noted that the necessary oxygen permeation rate is high and also that the mixture ratio is below the limit most of the time, just briefly reaching it midway through the ascent when the hydrogen and oxygen reach their peak levels. It should also be noted that even if these permeation rates are exceeded, it would be possible to provide an onboard nitrogen purge at a very minor system performance and cost impact.

For the second case, the oxygen leak rate is not considered and the allowable is determined by finding the rate at which the hydrogen must permeate so that an amount equal to 4% by volume at atmospheric pressure can accumulate in the compartment before it

DELTA 5-METER UPPER STAGE COMPOSITE HYDROGEN TANK PERMEABILITY ALLOWABLE ANALYSIS								
INTERSTAGE								
Case 1: Hydrogen concentration must be higher than 4% to account for reduced oxygen concentration								
			Oxygen tank surface area (cm^2):		401954			
	Oxygen leaks into interstage from LOX tank closures (scim):				40		**Base O_2 Amount**	
		Oxygen tank permeation rate ($scc/s/cm^2$):			1.55		23993446	
		Hydrogen tank aft dome surface area (cm^2):			323077			
	Hydrogen leaks into interstage from aft closure (scim):				20		**Base H_2 Amount**	
		Interstage open volume (cubic meters):			130		4798689	
	Hydrogen permeation through walls of composite tank ($scc/s/cm^2$):				0.75			
s	**Total Gas Present (scc)**	**N_2** (left from compartment purge)	**O_2** (from leaks and permeation)	**H_2** (from aft dome leaks and permeation)	**Amount Venting**	**O_2 Base Fraction**	**H_2 Required To Create Hazardous Condition**	**Difference Between Required Amount and Amount Present**
0	119967228	119967228	0	0	2065091	0.0000		
1	118767556	117902137	623041	242378	2065091	0.0260	184798593	184556215
2	117567883	115852093	1235249	480542	2065091	0.0515	93209646	92729104
3	116368211	113817140	1836592	714479	2065091	0.0765	62690608	61976129
4	115168539	111797320	2427041	944178	2065091	0.1012	47439289	46495111
5	113968867	109792678	3006562	1169626	2065091	0.1253	38295261	37125635
6	112769194	107803258	3575125	1390811	2065091	0.1490	32205053	30814242
7	111569522	105829106	4132696	1607720	2065091	0.1722	27860041	26252321
8	110369850	103870267	4679243	1820340	2065091	0.1950	24605921	22785581
9	109170177	101926787	5214732	2028658	2065091	0.2173	22079194	20050535
10	107970505	99998713	5739130	2232662	2065091	0.2392	20061766	17829104
11	106770833	98086094	6252402	2432337	2065091	0.2606	18414857	15982520
12	105571161	96188977	6754513	2627671	2065091	0.2815	17045949	14418279
•								
•								
45	65981975	42647784	16798978	6535213	2065091	0.7001	6853815	318602
46	64782303	41313002	16896248	6573053	2065091	0.7042	6814358	241305
47	63582631	39996051	16980681	6605900	2065091	0.7077	6780475	174576
48	62382959	38697025	17052208	6633726	2065091	0.7107	6752034	118308
49	61183286	37416019	17110762	6656505	2065091	0.7131	6728928	72423
50	59983614	36153134	17156272	6674209	2065091	0.7150	6711079	36870
51	58783942	34908468	17188663	6686810	2065091	0.7164	6698432	11622
52	57584269	33682127	17207863	6694279	2065091	0.7172	6690958	-3321
53	56384597	32474216	17213794	6696587	2065091	0.7174	6688652	-7934
54	55184925	31284845	17206378	6693702	2065091	0.7171	6691535	-2166
55	53985253	30114125	17185534	6685593	2065091	0.7163	6699651	14058
56	52785580	28962173	17151179	6672228	2065091	0.7148	6713071	40843
57	51585908	27829108	17103227	6653573	2065091	0.7128	6731892	78319
58	50386236	26715050	17041590	6629595	2065091	0.7103	6756241	126646
59	49186563	25620128	16966178	6600258	2065091	0.7071	6786271	186014
60	47986891	24544470	16876896	6565525	2065091	0.7034	6822172	256647
•								
•								
94	7198034	851590	4568994	1777450	2065091	0.1904	25199659	23422208
95	5998361	607271	3881206	1509884	2065091	0.1618	29665286	28155402
96	4798689	398202	3168041	1232445	2065091	0.1320	36343304	35110859
97	3599017	226838	2427732	944447	2065091	0.1012	47425785	46481338
98	2399345	96680	1657757	644908	2065091	0.0691	69453547	68808639
99	1199672	13469	853983	332220	2065091	0.0356	134823568	134491348
100	0	0	0	0	2065091	0		

Fig. 1 Permeation rate allowable for hazardous mixture prevention in the Delta IV 5-m interstage.

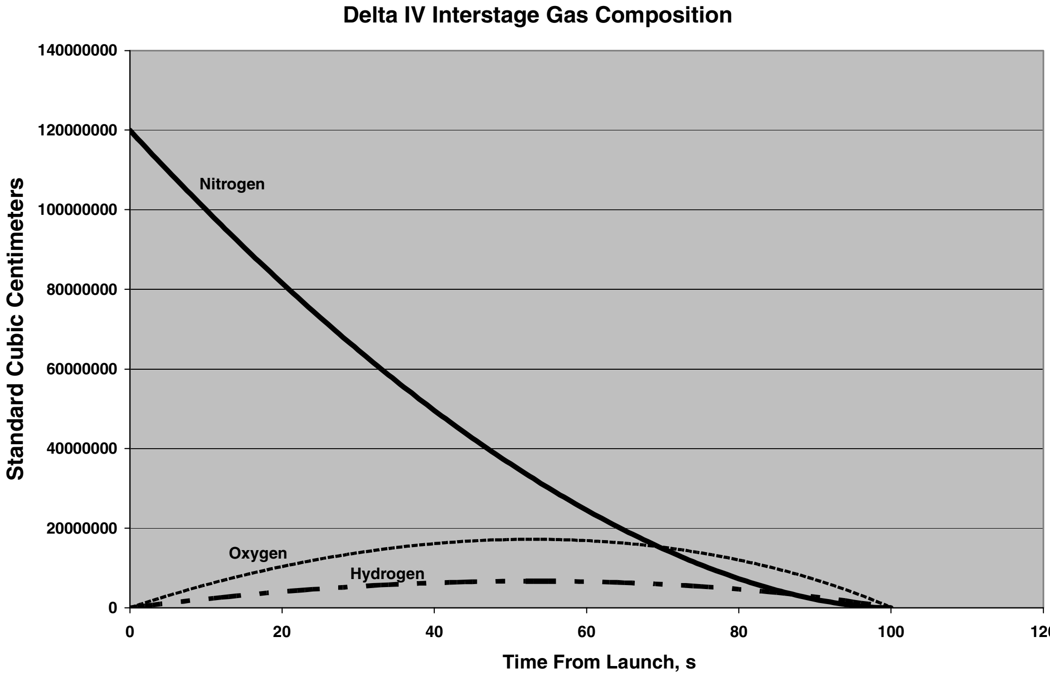

Fig. 2 Delta IV 5-m interstage open volume gas composition from launch to $T + 100$ s at permeation rates high enough to create a hazardous condition.

begins to decline due to venting. As stated above, this case is very conservative in that it is assumed that enough oxygen is always present and only considers hydrogen permeation. A similar spreadsheet and iterative procedure are used. The allowable permeation rate for this case on the Delta IV 5-m interstage is 0.423 scc/s/cm^2. Results calculated for both cases for several launch vehicle compartments are listed in Table 2.

3. *Operations Needs During Descent*

This allowable permeation rate would apply only to reusable launch vehicles so advanced that the orbital component would have its own propellant tanks (as compared to an orbiter that relies on separate tanks present only during boost, such as the space shuttle). And it might not even apply in that case. Designing large cryotanks for atmospheric reentry, descent, and landing would be very challenging. In regard to permeation, a key issue would be the question of tank internal pressure. System engineers would prefer to have tanks capable of withstanding reentry and landing loads unpressurized. Experience indicates, however, that such a design would incur a large weight penalty. If the tanks are to be pressurized for structural stability during descent and landing, where would the pressure come from? It might come from helium bottles on the vehicle; they would probably be onboard anyway to provide in-flight engine purges, and it may be reasonable to carry sufficient helium to pressurize the tanks as well. But another possibility, the most attractive from a weight efficiency standpoint, is to use residual propellant to provide pressure. If this option is taken, another permeability failure mode is introduced: the loss of too much propellant on orbit to adequately pressurize the tank. Necessary information for this calculation includes the tank volume, tank surface area, maximum mission duration, tank blowdown pressure, minimum tank pressure for reentry, and a margin on the reentry pressure.

Blowdown pressure is the pressure to which the tanks would be vented down shortly after MECO. The hydrogen losses due to permeation and leaks correspond to the difference between the blowdown pressure and the required reentry pressure plus margin.

An average equilibrium tank temperature on orbit is needed to compute quantities of gas present for given pressures. The temperature selected for this calculation is taken from an average structure temperature underneath blanket-type insulation. According to Boeing structural engineers that worked on the International Space Station, as an insulated structure orbits the Earth, passing in and out of its shadow, the temperature typically varies from about 110°F to −50°F.

Leakage rates for valves and tank closures were obtained from Delta IV propulsion personnel and cryogenic valve manufacturers. It turns out that leaks from these sources have relatively small effects. The calculated allowable permeation rate for this failure mode on Boeing's reusable orbiter design for the SLI program was 2.51×10^{-4} scc/s/cm^2.

The analysis does not, of course, account for any accidental depressurization. Such an accident would be possible due to impacts

Table 2 Maximum allowable hydrogen permeation rates to maintain nonhazardous mixtures in compartments

Compartment	Case 1 allowable permeation rates O_2 (scc/s/cm^2)	Case 1 allowable permeation rates H_2 (scc/s/cm^2)	Case 2 allowable permeation rates H_2 (scc/s/cm^2)
Delta IV			
5-m interstage	1.55	0.75	0.423
Payload adapters			
PAF 3518-5	Hazardous mixture unreachable		0.025
PAF 4572-5	Hazardous mixture unreachable		0.029
PAF 1575-5	Hazardous mixture unreachable		0.029
PAF 1666-5	Hazardous mixture unreachable		0.029
Common booster core centerbody	0.14	1.51	0.129
Common booster core engine section	Hazardous mixture unreachable		0.166
SLI intertank	0.016	0.048	0.0094

from micrometeoroids or orbital debris, or from a mechanical malfunction. The potential for these events would have to be seriously considered in the decisions about whether and how to pressurize the tanks.

B. Design-Dependent Failure Modes

By definition, design-dependent failure modes can be avoided by appropriate design. Nevertheless, some designers may consider that the advantages offered by a particular design feature may warrant its consideration even if it means having to deal with otherwise avoidable permeation failure modes. Also, analyzing these modes provides additional perspective on the permeation issue in general.

1. Maximum Core Pressure in Unvented Honeycomb Sandwich Core

This is the failure mode that doomed the X-33 tank. The allowable permeation rate is calculated by determining the rate at which hydrogen would have to permeate through the inner wall of an unvented honeycomb sandwich design to reach a specified allowable pressure later when the tank is warm. Necessary information for the calculation includes a core thickness, an allowable core pressure, a maximum on-orbit temperature, and a time interval for filling the tank and for ascent.

Core thickness for large tanks is expected to be in the range of 1 to 1.5 in. It was set at 1 in. for these calculations. Allowable core pressure is difficult to determine. The core pressure of the X-33 tank lobe that failed was 58 psi at the time. Meanwhile, other lobes sustained pressures up to 80 psi without failure. And Lockheed Martin Aerospace Corporation flatwise tensile strength test data at −423°F on specimens cut from the tank averaged 285 psi. Because of this uncertainty, allowable permeation rates for this mode have been calculated for a variety of pressures and these are given in Table 3. A safety factor of 1.4 was applied to the allowable core pressure. The maximum on-orbit temperature assumed for the tank wall was 110°F, based on the previously noted discussion with Boeing space station structures analysts.

It should be noted that these calculations assume that none of the hydrogen permeating into the core subsequently escapes (conservative) but also that no other species, such as nitrogen and oxygen from the surrounding air, are cryopumped into the core (unconservative), as postulated to have occurred on the X-33 tank.

As a point of reference, a theoretical maximum on-orbit pressure to be expected in this type of sandwich core was calculated. It is based on the amount of gas that would accumulate in the core if the pressure and temperature there reached equilibrium with the tank interior. The equilibrium core pressure was taken to be an expected operating pressure of 30 psi plus the head pressure at the bottom of the tank when it is full. The equilibrium core temperature was taken from X-33 tank test data provided in the failure investigation report. The expected maximum core pressure was then calculated by scaling the cold temperature equilibrium pressure upward for the temperature increase to 110°F using the ideal gas law. The estimated maximum core pressure is 140 psi.

2. Explosive Mixture in Sandwich Cores

For sandwich wall constructions, the accumulation of an explosive gas mixture in the sandwich core is a potential permeability failure mode. The likelihood of a viable ignition source inside the core is debatable and Boeing's preferred tank wall design concepts typically incorporate open cores that would be purged and vented. This is not only to sweep away any permeating hydrogen; a potentially more serious contaminant to eliminate is water. Moisture that accumulates in the core will be subject to cyclic freezing and melting, with potentially disastrous effects on skin-to-core bonds. A nitrogen purge is thus anticipated that, in addition to preventing moisture accumulation, will deprive this space of oxygen, just as in the large compartments.

As a result, the calculations for this allowable make the same very conservative assumption as the second case in the compartment analyses: that sufficient oxygen for combustion is always present regardless of the purge and that the "4% at atmospheric pressure" limit applies at all times for the hydrogen. The same sort of iterative stepwise analysis of the composition of the gas inside the core was conducted here as was done for the second case on the compartments.

Two cases were evaluated for each of the three core thicknesses using the large SLI hydrogen tank design geometry. The core was assumed to be open; no accounting was made for cryogenic insulation inside the core. In the first case, complete mixing inside the core along the length of the tank axis was assumed, just as in the compartments. In the second, a linear concentration gradient was assumed for the axial direction because it is likely that the core contents will vent at one end of the cylinder and the hydrogen concentration will be greater there. The calculated allowables are listed in Table 4.

Once again, it should be noted that this analysis includes the conservative assumption that oxygen is always present in sufficient quantity for the "4% at atmospheric" rule to apply and that this limit is reached only briefly midway through the ascent.

3. Explosive Mixture in Wing Fairings

Many reusable launch system concepts incorporate winged vehicles in which the liquid hydrogen tank is blended with the wings along its entire length with fairings. For sandwich-type wall constructions, permeation into the fairings is unlikely because the hydrogen would have to penetrate both walls of the sandwich. For single-wall construction, such as skin/stringer or some type of grid structure, the explosive mixture concern would exist for the fairings just as it would for the intertank and engine sections. As a result, this failure mode was assessed for the SLI vehicle design in the same way as for the other compartments.

The geometry of the fairing volumes and the permeating surface area were such that the hazard here was even less than that of the other compartments. When the same stepwise compartment composition analysis was performed using the conservative second case assumption that sufficient oxygen is always present (even though the fairings will likely be purged and there is no known source of oxygen leaks into the fairings), the calculated allowable permeation rate is 1.08×10^{-1} scc/s/cm^2.

4. Explosive Mixture Under TPS

In a reusable vehicle, much of the tank's cylindrical surface immediately outboard from the tank wall will be covered by some sort of thermal protection system, or TPS. The type of TPS will vary considerably over the surface, because of the variation in heat load and significant cost differences between different types. One of the

Table 3 Allowable permeation rates for sandwich core internal pressure

Core pressure, psi	Allowable permeation rate during prelaunch, scc/s/cm^2	Average allowable permeation for prelaunch and ascent, scc/s/cm^2
50	3.70E − 04	3.58E − 04
100	7.39E − 04	7.15E − 04
150	1.11E − 03	1.07E − 03
200	1.48E − 03	1.43E − 03
250	1.84E − 03	1.78E − 03

Table 4 Allowable permeation rates for sandwich core explosive mixture hazard

Core thickness, in.	Allowable permeation rate based on complete mixing, scc/s/cm^2	Allowable permeation rate with concentration gradient, scc/s/cm^2
1.0	4.76E − 03	1.91E − 03
1.5	6.40E − 03	2.91E − 03
2.0	8.53E − 03	3.88E − 03

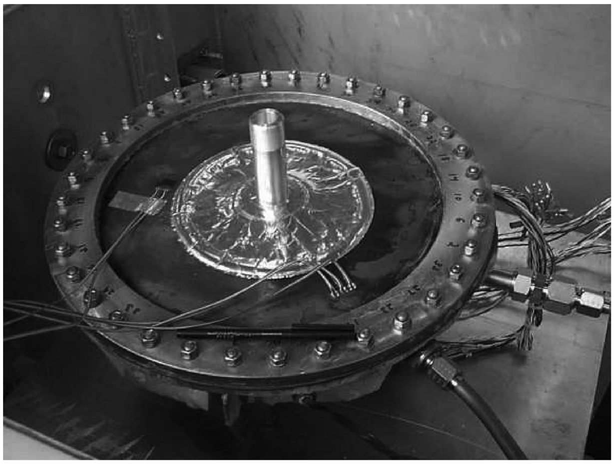

Fig. 3 Disk specimen and cryostat for NASA MSFC biaxial cycling and permeation rate testing.

fundamental characteristics of TPS is the method of attachment to the structure: adhesive bonded to the surface or mechanically attached. The mechanically attached type is often set off from the tank wall by some discrete distance to aid in the thermal management of the overall tank and TPS system. If the tank construction is single wall, as in skin/stringer or grid structure, and the TPS is mechanically attached with a significant standoff distance, then a concern arises about the development of an explosive mixture in the standoff space due to hydrogen permeation.

Because of the similarity to the sandwich core explosive mixture mode described above, including a similarity in the thickness of the core and the standoff distance, the allowables calculated for the sandwich core are probably a reasonable approximation for the allowables for this mode as well.

III. Testing

Relevant permeation test data, even on fairly simple panels or coupons, is very difficult to obtain. Proper specimen preconditioning is necessary and it alone is difficult to accomplish. The primary cause of hydrogen permeation through composites is believed to be microcracks that form in the resin matrix. The microcracks develop from the stress and the extreme temperatures to which the material is subjected. At liquid hydrogen temperature (−423°F), the tensile stress that arises in the resin matrix due to the CTE difference between resin and fiber will consume most of the strain capability of the resin before any mechanical load is applied. When the tank is pressurized, significant biaxial tensile stresses develop in the wall that add more strain to the matrix. Additional stress and strain develop due to inertial and aerodynamic loading. Appropriate specimen preconditioning, therefore, requires that the test specimens be subjected to a biaxial tension load at extremely cold temperatures, for a large number of cycles. For reusable vehicle tanks, approximately 2500 cycles will be required after multiplying the number of required missions by appropriate factors in typical qualification practice. Even for expendable tanks, as many as 120 cycles may be necessary for qualification.

The first challenge is to develop an apparatus for inducing the biaxial load at cold temperature in the composite specimen. The second is to develop a cycling methodology for the test specimen to duplicate or approximate the thermal and mechanical stress life of a real tank. Finally, the remaining challenge is to measure the permeability while the specimen is in the various mission stress states. This means incorporating the permeability measurement equipment with the apparatus that induces biaxial load at cold temperature.

Because of the difficulty and expense of accomplishing all of this, permeability testing was limited for many years to uniaxially loaded coupons, sometimes at cold temperature, sometimes not. But in the wake of the X-33 tank failure, NASA and its industry partners undertook the task of developing methods and equipment to obtain the necessary data.

In one approach, NASA Marshall Space Flight Center (MSFC) developed an apparatus to apply a biaxial load to a disk-shaped panel directly by using the panel as a closure on a high-pressure cryostat. A test panel, shown installed in the cryostat, is illustrated in Fig. 3. Panel thicknesses were approximately 0.040 in. (8 plies).

This apparatus required careful design, analysis, and manufacturing of both panel and fixture at the panel perimeter to prevent excessive bending stresses, provide adequate sealing, and avoid slippage when the cryostat is pressurized. The Boeing Company and Northrop Grumman fabricated panels from many different candidate composite tank materials and supplied them to NASA MSFC. Twenty-one panels were eventually cycled and tested for hydrogen permeation. Although technical difficulties precluded reaching the 2500 cycles envisioned for the RLV tank qualification, significant biaxial cycling at liquid hydrogen temperature was achieved. Table 5 documents the cycle quantity and strain level for several of the panels. Figure 4 is a compilation of data that shows the maximum hydrogen permeation rates through many of the panels and the maximum strain to which they had been subjected. As the data indicate, no permeation rates above 3.9×10^{-5} scc/s/cm^2 were observed and most were below 8×10^{-6} scc/s/cm^2.

In another approach, investigators at The Boeing Company attempted to exploit the anisotropy of composite material to simulate biaxial stress cycling by sequential uniaxial cycling. Lamination theory calculations confirm that for some laminate arrangements, the transverse stress expected in a portion of the plies under the biaxial load can be duplicated with a uniaxial load while the transverse stress in the other plies is kept very low. Therefore, by stressing a panel in one direction and then the other, the transverse strain history of biaxial loading can be approximated. This procedure does not work well with all layup arrangements and does not capture the effects of combined shear and transverse stress in biaxial loading but it can be accomplished with a much simpler apparatus and test panel assembly. One of the test panels is shown in Fig. 5 and the cycling apparatus is illustrated in Fig. 6. The cryostat that encloses the gauge

Table 5 Cycle quantities and strains of panels loaded biaxially at −400°F at NASA MSFC. UTL stands for ultrasonic tape layup

−400°F cycling history of selected panels in MSFC apparatus													
			Cycles to approximate biaxial microstrain at −400°F										
Panel no.	Material	Total cycles	1000		2000		3000		4000		5000		6000
NG-01	IM7/977-2, 5.2 mil/ply	39				5	19	10	5				
NG-03	IM7/3900-3	16			5	11							
NG-04	IM7/977-2, 2.6 mil/ply	37			5		5	5	15	7			
NG-06	IM7/977-2, 2.6 mil, UTL, vac.	46			9		5	21	5		6		
NG-07	IM7/977-2, aluminum liner	27	5	8			4				10		
B-05	IM7/5250-4, urethane liner	14					5		9				
B-07	IM7/5250-4, aluminum liner	15				5			5		4	1	
B-09	IM7/8551-7	30				7		5	2	15	1		

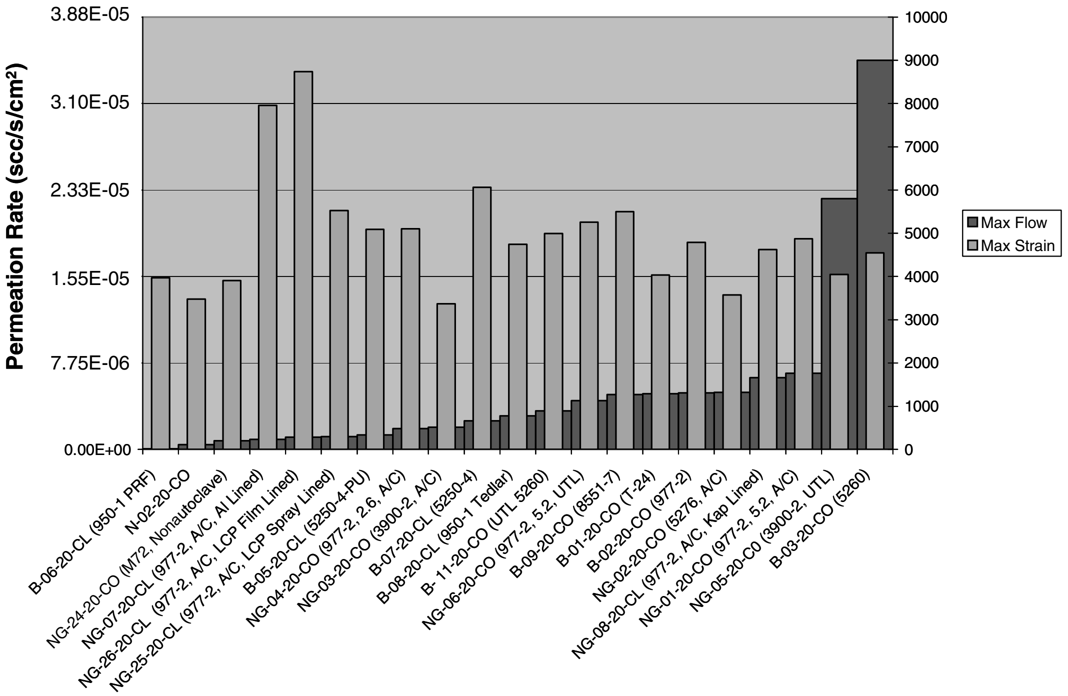

Fig. 4 Composite permeation data from the NASA SLI program.

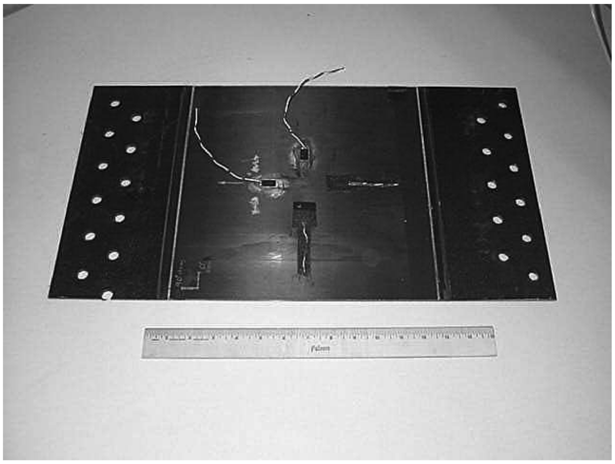

Fig. 5 Flat panel for sequential biaxial cycling.

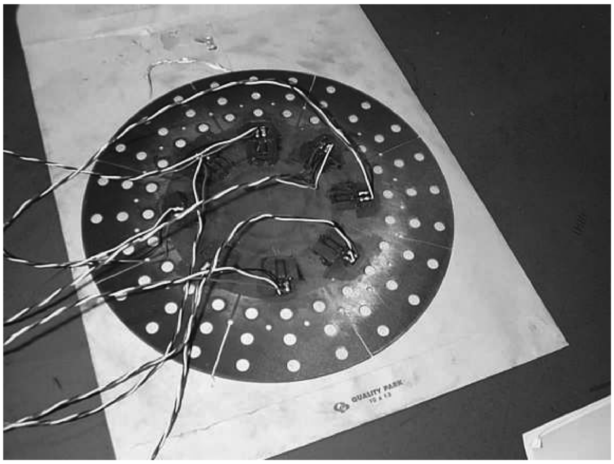

Fig. 7 SRI permeation test specimen.

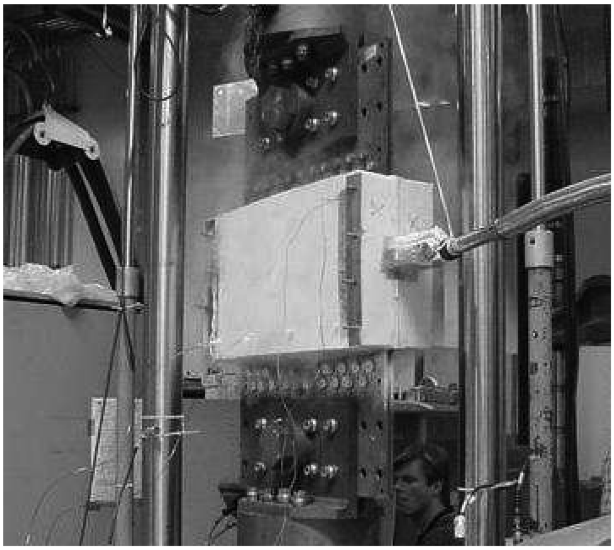

Fig. 6 Uniaxial cycling of flat panel at −423°F.

section of the panel can be seen in Fig. 6. Liquid helium flow through the cryostat was regulated to maintain liquid hydrogen temperature around the panel. Five panels, each about 0.050 in. thick (10 plies), were subjected to 2500 tensile stress cycles to 5000 microstrain at −420°F using this procedure. The strain level was inadvertently higher than it should have been. 5000 microstrain is a reasonable value for a working level uniaxial strain. For biaxial tension, a more likely working level will be 3500 microstrain.

After the sequential cycling, circular coupons were cut from the panels for permeation testing at the Southern Research Institute (SRI) in Birmingham, Alabama, the same laboratory in which permeation testing was conducted during the X-33 tank failure investigation. A test specimen, into which integral tabs have been machined for the grips used to apply multiaxial loads, is shown in Fig. 7.

Permeation testing was conducted at SRI on the five cycled panels and on some uncycled panels. The best performer of the cycled panels was made of IM7/977-2 material. A plot of permeation rate vs microstrain at three different temperatures is shown for this panel in Fig. 8. As the data indicate, the permeation rate varied from 2.17×10^{-3} scc/s/cm^2 under no strain at 75°F to 8.68×10^{-2} scc/s/cm^2 under 1500 microstrain at −420°F [2].

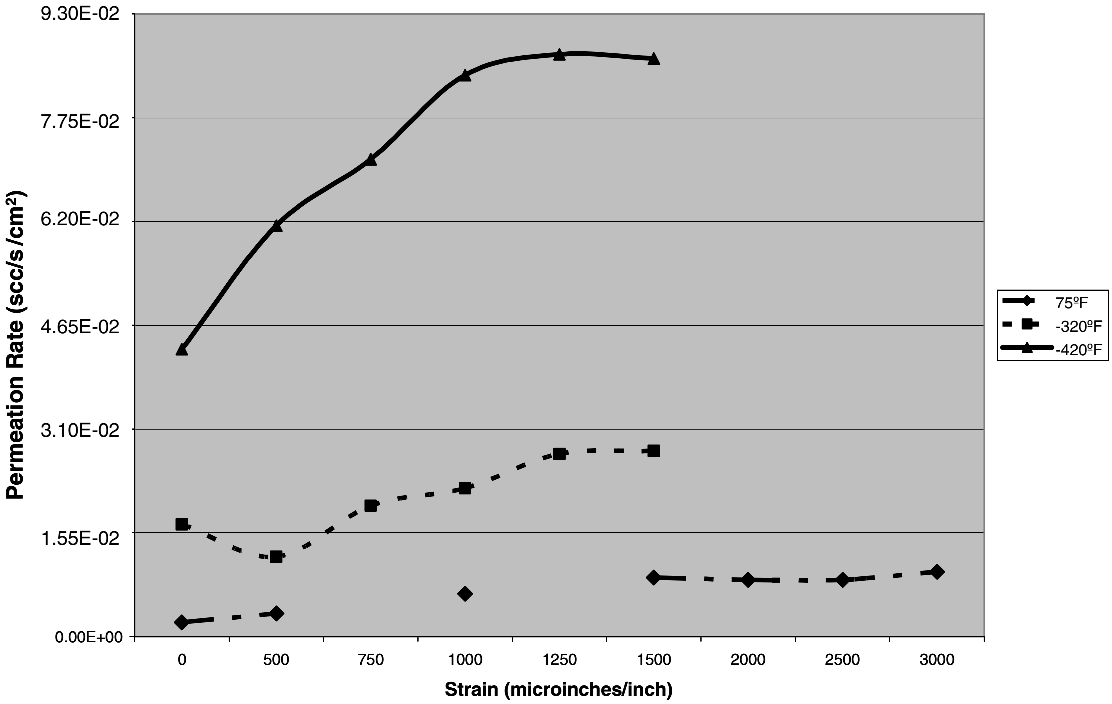

Fig. 8 Permeation rate data from SRI on IM7/977-2 panel subjected to sequential uniaxial cycling.

These panels were much more permeable than the panels tested in the other apparatus at NASA MSFC. This is believed to be at least partly due to the much larger number of cycles these panels experienced but may also be a result of the significantly different way in which the stress was induced during cycling.

Although the permeation rates through the SRI panels might seem alarmingly high and are within the range of many of the calculated allowable rates discussed above, several mitigating factors should be kept in mind. First, only a few of the tank material candidates were tested in the SRI apparatus and they were not necessarily the best. The testing at MSFC revealed that panels made with thinner plies (2.5 mil per ply rather than 5 mil per ply) were significantly more resistant to microcracking and permeation; panels of this type were not tested at SRI. Second, the indirect method of simulating biaxial stress by sequential uniaxial stress may have been inadequate for truly representative cycling of the material. Third, the strain level to which the panels were cycled was much higher than the expected working level for biaxial loading of about 3500 microstrain. SRI permeation data on uncycled panels indicate that there is a large jump in permeation under a biaxial load at about 3800 microstrain. Fourth, the calculated allowable permeation rates discussed above are allowable *average* rates over a relevant time period. Most of the tank surface is not under maximum load and not at −420°F for most of that time. Significantly higher permeation rates in localized areas could be tolerated for brief periods. Finally, the calculated allowable rates are typically based on conservative or very conservative assumptions and could easily be increased with more realistic assumptions or with generally minor system modifications.

IV. Conclusions

An inappropriate lesson may have been drawn from the X-33 tank incident. Although permeation played a key role in the failure, it is important to note that the wall configuration chosen for that tank made it uniquely vulnerable to the particular failure mode that arose. Other wall configurations are possible that would not fail in the way the X-33 tank did and permeation may not be the obstacle that the X-33 tank problem suggested.

The calculated allowable permeation rates discussed above are significantly higher than the levels composite tank developers have historically tried to meet and also appear to be significantly higher than most of the rates measured on composite panels in the NASA SLI program. Moreover, most of the calculated allowables are fairly soft in that they are either based on conservative assumptions or could be relieved through modest vehicle system enhancements. All of which tends to indicate that, with proper design choices, hydrogen permeation should not be a serious impediment to the development of composite cryogenic tanks.

References

[1] Final Report of the X-33 Liquid Hydrogen Tank Test Investigation Team, NASA Marshall Space Flight Center, May 2000.

[2] Stokes, E., "Hydrogen Permeability of Polymers and Polymer-Based Composites," NASA Marshall Space Flight Center, SRI-ENG-01-36-A392.0, Final Report to July 2001.

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